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STUDY OF FATIGUE CRACK INITIATION FROM FLAWS USING FRACTURE MECHANICS THEORY

R. G. FORMAN

TECHNICAL REPORT AFFDL-TR-68-100

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STUDY OF FATIGUE CRACK INITIATION FROM FLAWS USING FRACTURE MECHANICS THEORY

R. G. FORMAN

FOREWORD

This report describes an experimental and theoretical study on the fatigue crack initiation from flaws in cyclic loaded structures. The experimental work was conducted from July 1966 to August 1967, by the Structures Test Branch, Structures Division, Air Force Flight Dynamics Laboratory. The test engineer for the program was Mr. R. L. Schneider. Mr. H. E. Andrews, test technician, prepared the specimens, conducted the tests, and recorded the data.

This report and the initial test program plan was written by Mr. R. G. Forman, Aerospace Engineer, Theoretical Mechanics Branch, Structures Division, Air Force Flight Dynamics Laboratory, under Project No. 1467, "Structural Analysis Methods," Task 146704, "Structural Fatigue Analysis," with Mr. Robert M. Bader acting as Project Engineer.

The manuscript was released by the author in May 1968 for publication as a technical report.

This technical report has been reviewed and is approved.

FRANCIS J. JANIK
Chief, Theoretical Mechanics Branch
Structures Division
Air Force Flight Dynamics Laboratory

ABSTRACT

This report presents theoretical and experimental results on fatigue crack initiation from flaws in cyclic loaded structures. The results indicated that the fracture mechanics stress-intensity-factor range, ΔK, is the governing parameter for inducing fatigue crack initiation from a flaw. Theoretical crack growth analysis indicated that when initiating an "engineering size" fatigue crack from a flaw, most of the cyclic behavior was crack growth and only a small part was nucleation.

This abstract is subject to special export controls and each transmittal to foreign governments or foreign nationals may be made only with prior approval of the Air Force Flight Dynamics Laboratory (FDTR), Wright-Patterson AFB, Ohio.

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SYMBOLS

C,n	Material constants for crack propagation
F( )	A function
K	Fracture mechanics stress-intensity factor
ΔK	Stress-intensity-factor range (maximum K - minimum K in a load cycle)
K_a	Apparent fracture toughness for blunt crack
K_c	Critical stress-intensity factor for fracture
N	Load cycles
N_i	Number of load cycles for fatigue crack initiation
R	Radius of hole, minimum K/maximum K in a load cycle
Y	Material tensile yield stress
a	Flaw length (half length for central slit), radius of penny-shaped flaw
x,y,z	Cartesian coordinates
r, θ	Polar coordinates
r_p	Dimension of yield, or plastic, zone at $\theta = 0$ for finite root radius flaws
t	Thickness of sheet
w	Theoretical yield-zone width for $\rho = 0$ flaws
w_c	Theoretical critical yield-zone width for $\rho = 0$ flaws
σ_0	Applied stress on sheet
$\Delta\sigma_0$	Applied stress range in a load cycle (maximum σ_0 - minimum σ_0)
σ_x, σ_y, σ_z τ_{xy}, τ_{xz}, τ_{yz}	Normal and shear stresses
σ_0	Tangential stress
$\Delta\sigma_t$	Tangential stress range in a load cycle
β	Angular position of notch on hole boundary
ρ	Radius of curvature at crack tip
ν	Poisson's ratio

SECTION I

INTRODUCTION

Data exists which indicates that fatigue cracks in cyclic loaded structures often originate from flaws. The flaws originate either during manufacture of the structure in the form of tool marks and weld flaws, or during usage of the structure, as nicks or scratches. The current practice in estimating the fatigue life of a flawed structure is to conduct crack propagation analysis and to assume that crack growth begins with the first load cycle. Recently improved theories exist for crack growth behavior (e.g., see References 1 and 2), and when environmental effects are taken into account, the remaining life of fatigue-cracked structures can usually be predicted.

The basic difficulty in using crack propagation analysis alone, however, is that all flaws are arbitrarily assumed to be so sharp or large that the crack initiation phase can be neglected. The analysis is questionable, for example, for very fine surface scratches in aluminum structures where experience has shown that these scratches do not have a measurable effect on the fatigue life. Very little is known about the effect of flaw size and flaw tip radius on fatigue initiation, and this problem needs to be resolved before the cyclic behavior of real structures can be adequately understood.

The purpose of the study in this report, then, was to investigate the initiation phase of fatigue cracks which originate from flaws. The study was essentially an experimental effort using centrally notched sheets of 7075-T6 aluminum alloy with various flaw geometries, sizes, and root radii. The experimental results were analyzed using the stress-intensity-factor concept from fracture mechanics theory. Corrections were made to account for the finite root radii of the flaw tips.

SECTION II

SUMMARY

A theoretical and experimental study has shown that the fracture mechanics stress-intensity-factor approach is applicable to the analysis of fatigue crack initiation from flaws. The relative stress-intensity-factor range, $\Delta K/K_a$, appeared to be the governing parameter for causing initiation of an ''engineering size'' fatigue crack from a flaw in a cyclic loaded structure. Initiation of an ''engineering size'' crack was difficult to define, however, and theoretical analysis of the data indicated that a large percentage of the cyclic behavior (i.e., 90%) was probably crack propagation from an unknown micro-size to an ''engineering size'' crack and very little was nucleation. Finally, a limiting size of flaw was determined which would not have a measurable effect on the fatigue life of structures manufactured from 7075-T6 aluminum alloy.

SECTION III

TECHNICAL APPROACH

Currently, general agreement exists with the Paris theory (Reference 3), which states that the stress-intensity-factor range, ΔK, may be viewed as the driving force for fatigue crack propagation. Since crack growth rate is governed by ΔK, it is reasonable to assume that the number of cycles required to initiate a fatigue crack from a sharp notch or flaw is also determined by ΔK. Limited data exists which shows that this is indeed true. Figure 1, for example, shows experimental results for crack initiation from flaws in butt-welded HY-80 steel strips. Use of the stress-intensity factor for a penny-shaped internal flaw appears to give very consistent results in the data for cycles to crack initiation.

One problem in either an experimental or theoretical study of fatigue crack initiation is the proper definition of crack initiation. Usually, there exists a micro-crack growth phase and a macro-crack growth phase in fatigue (e.g., see the related study of Schijve, Reference 4), and the initiation of an "engineering size" could be considered as the crack size when the macro-crack growth phase commences. This could be a crack length of .002 to .003 inch, which is about the minimum size that can be clearly distinguished under an optical microscope, or it can be a length of .010 inch, which is approximately the minimum length that can be distinguished with the naked eye. For the present study, both definitions were used, and the results indicated that the technical approach was applicable for both lengths.

In crack propagation analysis, a valid approach in describing either the crack growth rate or the cycles to failure is to assume that the behavior is a function of the relative stress-intensity-factor range, $\Delta K/K_c$. The validity of this appraoch was confirmed in the current study for determining crack initiation behavior. One difference, though, is that K_c, or apparent K_c, is not a constant but depends upon the root radius of the notch tip. A new ratio, $\Delta K/K_a$, was defined to account for the variation where K_a is the apparent fracture toughness which is corrected for the finite root radius.

The correction factor used to correct K_c for the effect of the finite root radius was derived from a theoretical analysis of the yield zone at the tip of a blunt crack. The analysis is described in the Appendix, but the results of the analysis is that a functional relationship exists between the yield-zone size and the crack tip root radius. The relationship is shown graphically in Figure 2 for the cases of both plane stress and plane strain.

A series of tests were conducted on centrally notched sheets of 7075-T6 aluminum alloy to measure the effect of crack root radius on K_c. The results are shown in Figure 3 and listed in Table I where $\beta = 90°$ and $N_i = 1$. The results indicate that the apparent fracture toughness can be determined from the following formula:

$$K_a = \left(\frac{r_p}{w_c} \right) K_c \tag{1}$$

where (r_p/w_c) is the relative size of the yield zone, as shown in Figure 2.

In Figure 3 is shown an experimental curve for the data of Mulherin, Armiento, and Markus (Reference 5) for 60-degree v-shaped edge-notched specimens. Their data indicates a much greater increase in apparent fracture toughness with root radius than the centrally notched specimen data exhibits. The discrepancy is difficult to evaluate because neither notch geometry has a true solution for the stress distribution. For instance, the central notch geometry is analyzed by assuming a flattened elliptical hole in a plate. For the edge notch we assume a hyperbolic notch geometry. No solutions exist for either the case of an edge notch with straight edges for which the root radius is independent of the flank angle or for the central slit with variable root radii. For the present study, the central notch configuration was assumed to give the most accurate representation of apparent fracture toughness, and this configuration was used for the basis of Equation 1.

SECTION IV

TEST PROGRAM

All tests were conducted on .050-inch-thick sheets of bare 7075-T6 aluminum alloy. The size and geometry of the specimens are shown in Figure 4. The grain direction of the material was in the lengthwise direction of the specimen, or parallel to the direction of load.

The specimens were all tested in the same hydraulic loading machine for both the static and fatigue tests. The cyclic loading was a sinusoidal type of loading with frequencies between one and four cycles per second.

Fatigue crack initiation was determined by visual observation using a forty power stereomicroscope. The specimen surfaces were polished in the area of the notch tips to assist in distinguishing the smallest visible fatigue crack. After fatigue crack initiation was obtained, the crack size was measured with a forty power toolmaker's microscope. The sizes varied between .005 and .015 inch in length, and, thus, crack initiation was defined as the cycle at which an approximate .010-inch-long crack was first observed.

The smallest flaw tip radius for any specimen was .001 inch. This would probably be close to the minimum tip radius for an actual flaw in a structure because the radius was obtained by sawing with a razor blade. The sharpest razor-blade-cut flaws (.001 and .002 inch tip radius) were made with a special sawing machine made for the test program. The .004-inch-radius flaws were made by sawing manually. Flaws, .007-inch and larger, were obtained by drilling holes and cutting slots to the holes. The radii were checked with the toolmaker's microscope and the tolerance was found to be approximately ±.001 inch for the drilled holes and ±.0005 inch for the razor-blade cuts. These tolerances, or accuracies, should be applied to all listed values for the tip radii in Table I.

Both mechanical methods, such as drilling, and electrical discharge methods (EDM) were attempted to determine the best procedure for introducing the

notches in the specimens. The EDM method gave a significantly more accurate and better notch root, but the mechanical method was used because flaws introduced by this method would be more typical of actual flaws, such as tool marks or scratches.

SECTION V

TEST RESULTS

The test results for the notched 7075-T6 sheet specimens are shown in Figure 4, and the detail data is listed in Table I. The data is essentially for three types of notch geometry. Each geometry was analyzed using a different formula for the stress-intensity factor. The formula for each geometry was obtained from Reference 6 and they are given as follows:

(1) Central slit ($R = 0$, $\beta = 90°$):

$$\Delta K = \Delta\sigma_0 \sqrt{\pi a} \qquad (2)$$

(2) Notched circular hole with $\beta = 90°$):

$$\Delta K = \Delta\sigma_0 \sqrt{\pi a} \left[F\left(\frac{a}{R}\right)\right] \qquad (3)$$

where $F\left(\frac{a}{R}\right)$ is tabulated as follows:

a/R	$F\left(\frac{a}{R}\right)$
0.00	3.39
0.10	2.73
0.20	2.41
0.30	2.15
0.40	1.96
0.50	1.83
0.60	1.71
0.80	1.58
1.00	1.45

(3) Notched circular hole with β = 45° (small edge crack in a sheet subjected to uniaxial tension):

$$\Delta K = 1.12\, \Delta\sigma_t \sqrt{\pi a} \tag{4}$$

where

$$\sigma_t = \sigma_o \quad \text{for } \beta = 45°$$

Although three different notch geometries with varying size and tip radius were tested, the results indicated that $\Delta K/K_a$ is the main governing parameter for fatigue-crack initiation from a blunt flaw. The results were consistent for all specimens tested, and especially for flaws with a length-to-tip-radius ratio, a/ρ, greater than 5.

The experimental results shown in Figure 4 are for cycles to crack initiation ranging from unity to greater than 2 x 10^6. The results consisted of both plastic and elastic deformation at the flaw tips. Plastic deformation was assumed to occur when the maximum stress was greater than the tensile yield stress and elastic deformation when the maximum stress was less than the tensile yield stress.

To determine whether the deformation was elastic or plastic, two methods of analysis were used: one was to use the following solution (Reference 7) for the maximum stress at the boundary of an elliptical hole in a large plate subjected to uniaxial tension:

$$\sigma_t = \sigma_o + 2\sigma_o \sqrt{\frac{a}{\rho}} \tag{5}$$

Rewriting Equation 5 to be in terms of the stress-intensity factor gives the following equation which can be used for determining whether the maximum stresses are greater or less than the tensile yield stress:

$$\sigma_t = \sigma_o + \frac{2K}{\sqrt{\pi\rho}} \tag{6}$$

Another useful approach which gives nearly the same results in determining type of deformation is to note that elastic deformation occurs when ρ/w is greater than 8.0 for plane stress, and 6.5 for plane strain. For these values,

Table II shows the yield-zone boundary to coincide with the notch tip boundary, or, in other words, the notch tip is at the onset of yielding.

For the specimens that had no yielding at the notch tip, the maximum stresses were calculated by using Equation 6. Comparisons were made between the data in Table I and the fatigue data in References 12 and 13. One interesting result was that the endurance limit stress for the flawed specimens was approximately 60,000 psi at flaw tip. This is double the endurance limit stress for smooth or unnotched specimens of the same material. The results are in agreement then with current fatigue data, which shows that very sharp notches have less effect on fatigue strength than might be expected in view of their high theoretical stress-concentration factors.

For the data shown in Figure 4 and listed in Table I, crack initiation was defined as the load cycle when a .010-inch crack was first observed. A question arises, however, as to what percentage of the cyclic behavior was nucleation of a fatigue crack and what percentage was propagation to the "engineering size" crack. An estimate of the percentage can be obtained by using the crack growth theory presented in Reference 1. The theory states that crack growth per load cycle is given by the equation

$$\frac{da}{dN} = \frac{C(\Delta K)^n}{(1-R)K_c - \Delta K} \tag{7}$$

For the material tested, Figure 3 indicates that K_c equals 50,000 psi $\sqrt{\text{in}}$, and from Reference 1, n equals 3.0 and C equals 5×10^{-13}. We assigned da the value .010 inch, then calculated dN for different values of $\Delta K/K_c$. The results are shown by the dashed curve in Figure 4. The curve indicates that, theoretically, a large percentage of the behavior was crack propagation and only a small percentage was nucleation. In fact, the theoretical crack propagation curve is a very good representation of the lower boundary of the data. For flaws where the ratio a/ρ is greater than 5, a satisfactory approach for predicting cycles to initiation of an "engineering size" crack is to use crack propagation theory in which it is assumed that crack growth begins with the first load cycle.

To determine whether or not similar results occur for a different material, Manson's data (Reference 8) for 2014-T6 aluminum alloy was put in the form of $\Delta K/K_a$ versus N_i and plotted in Figure 5. For this data, N_i was for the initiation of an average crack length of .0025 inch. The results are very similar to the 7075-T6 aluminum alloy results, and the crack propagation analysis shows that for this defined crack length, most of the behavior was also crack propagation. The analysis was made using K_c equal to 30,000 psi $\sqrt{in}$ (Reference 9); C equal to 3×10^{-13}, and $n = 3.0$, both of which were obtained from the data in References 10 and 11.

Finally, the calculations resulting from $\Delta K/K_a$ versus N_i from Figure 4 were used in an analysis to determine the size limit of a flaw which would not have an effect on the fatigue life. The results of the analysis are shown in Figure 6. A comparison of the crack initiation curves with unnotched S-N fatigue curves shows that a flaw size of approximately .005 inch would be the limiting size before fatigue life in 7075-T6 aluminum sheet material would be affected. Smaller flaws than .005 inch could be neglected.

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APPENDIX

THE YIELD ZONE AT THE TIP OF A BLUNT CRACK

The elastic stresses in an infinite plate which contains a blunt crack and is subjected to uniaxial tension perpendicular to the crack direction can be found by the following relations obtained from Reference 15:

$$\sigma_x = \frac{K}{(2\pi r)^{1/2}} \cos\frac{\theta}{2}\left[1 - \sin\frac{\theta}{2}\sin\frac{3\theta}{2}\right] - \frac{K}{(2\pi r)^{1/2}}\frac{\rho}{2r}\cos\frac{3}{2}\theta \qquad (8)$$

$$\sigma_y = \frac{K}{(2\pi r)^{1/2}} \cos\frac{\theta}{2}\left[1 + \sin\frac{\theta}{2}\sin\frac{3\theta}{2}\right] + \frac{K}{(2\pi r)^{1/2}}\frac{\rho}{2r}\cos\frac{3}{2}\theta \qquad (9)$$

$$\tau_{xy} = \frac{K}{(2\pi r)^{1/2}} \sin\frac{\theta}{2}\cos\frac{\theta}{2}\cos\frac{3\theta}{2} - \frac{K}{(2\pi r)^{1/2}}\frac{\rho}{2r}\sin\frac{3}{2}\theta \qquad (10)$$

If the yield-zone size is small compared to the region in which the elastic stresses given by the above equations are valid, then outside the yield zone these stresses are still a reasonable approximation. If they are accurate up to the boundary of the yield zone, an estimate of the size and shape of the yield zone may be obtained.

By substituting the stresses, as defined in Equations 8, 9, and 10, into the "Distortion Energy" yield criterion that is,

$$\frac{1}{2}(S_x^2 + S_y^2 + S_z^2) + (\tau_{xy}^2 + \tau_{xz}^2 + \tau_{yz}^2) = \frac{Y^2}{3} \qquad (11)$$

where

$$S = \frac{\sigma_x + \sigma_y + \sigma_z}{3}$$

and

$$S_x = \sigma_x - S$$
$$S_y = \sigma_y - S$$
$$S_z = \sigma_z - S$$

the following equation is obtained for the radius vector, r, of the locus of points on the boundary of the yield zone:

$$r^3 - wF(\theta)r^2 - \frac{3}{4} w\rho^2 = 0 \tag{12}$$

In Equation 12

$$w = \frac{1}{2\pi}\left(\frac{K}{Y}\right)^2$$

and is the yield zone width for $\rho = 0$; the expression for plane stress is

$$F(\theta) = \cos^2\frac{\theta}{2}\left[1 + 3\sin^2\frac{\theta}{2}\right]$$

and for plane strain is

$$F(\theta) = \cos^2\frac{\theta}{2}\left[(1-2\mu)^2 + 3\sin^2\frac{\theta}{2}\right]$$

Equation 12 is an algebraic cubic equation which has one real root. The root has the following form when written in terms of r/w, the relative yield-zone size:

$$\frac{r}{w} = \frac{F(\theta)}{3} + \left[\left(\frac{F(\theta)}{3}\right)^3 + 2Q + 2\sqrt{Q\left(\frac{F(\theta)}{3}\right)^3 + Q^2}\;\right]^{1/3} + \left[\left(\frac{F(\theta)}{3}\right)^3 + 2Q - 2\sqrt{Q\left(\frac{F(\theta)}{3}\right)^3 + Q^2}\;\right]^{1/3} \tag{13}$$

where

$$Q = \frac{3}{16}\left(\frac{\rho}{w}\right)^2$$

Equation 13 indicates that r/w is a function only of the relative notch root radius, ρ/w. For comparison, the size and shape of the relative yield zone for a blunt notch and a sharp notch are shown in Figure 7.

By setting $r = r_p$ for $\theta = 0$, the width of the yield-zone boundary can be determined for different notch root radii. The widths were calculated for important ranges of ρ/w, and the results are given in Table II. The comparison of r_p/w versus ρ/w for both plane stress and plane strain is shown in Figure 2.

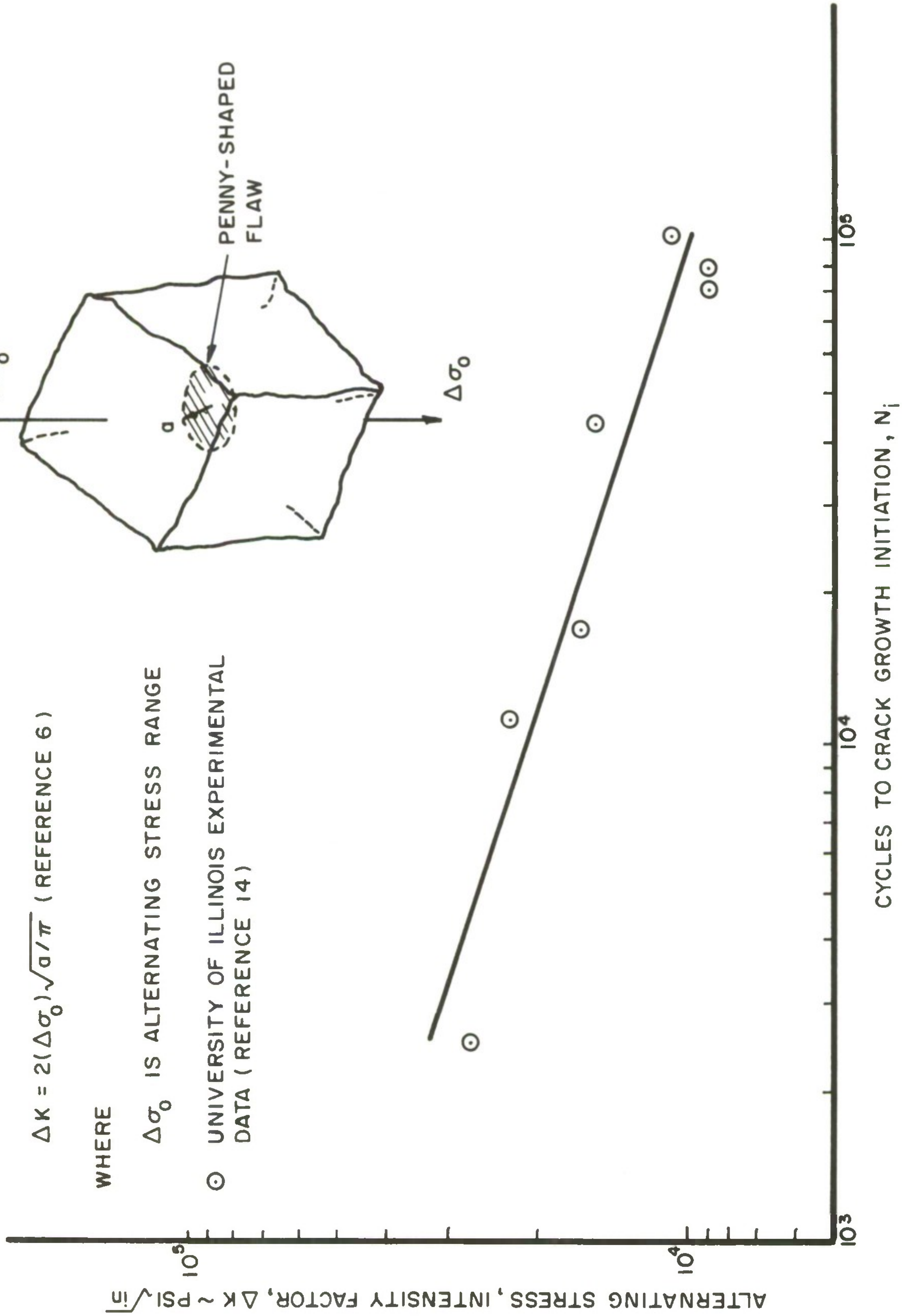

Figure 1. Variation in K with N_i for Flaws in Butt-Welded 3/4-Inch-Thick HY-80 Steel Strips

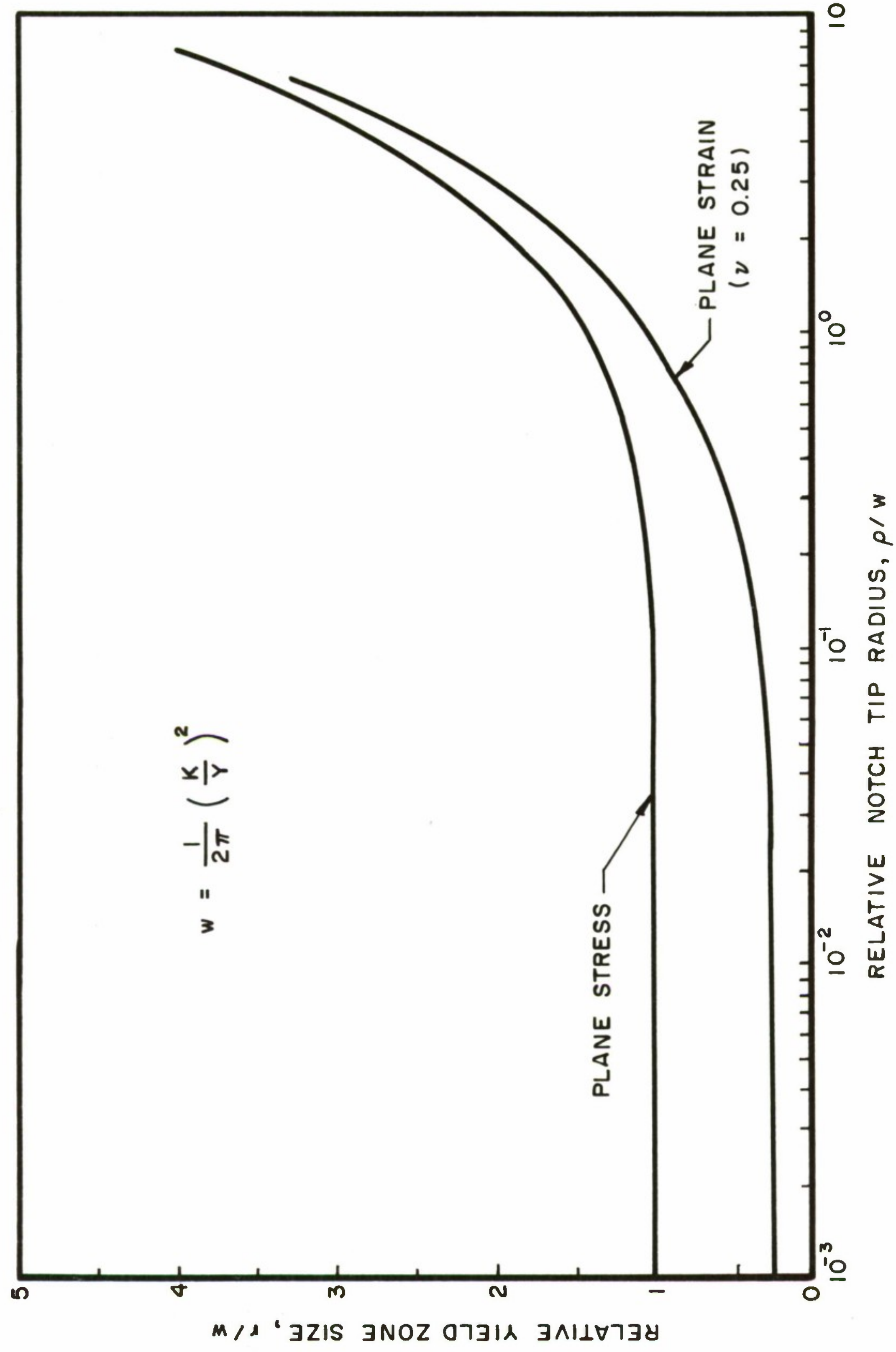

Figure 2. Variation in Yield-Zone Size with Notch Tip Radius

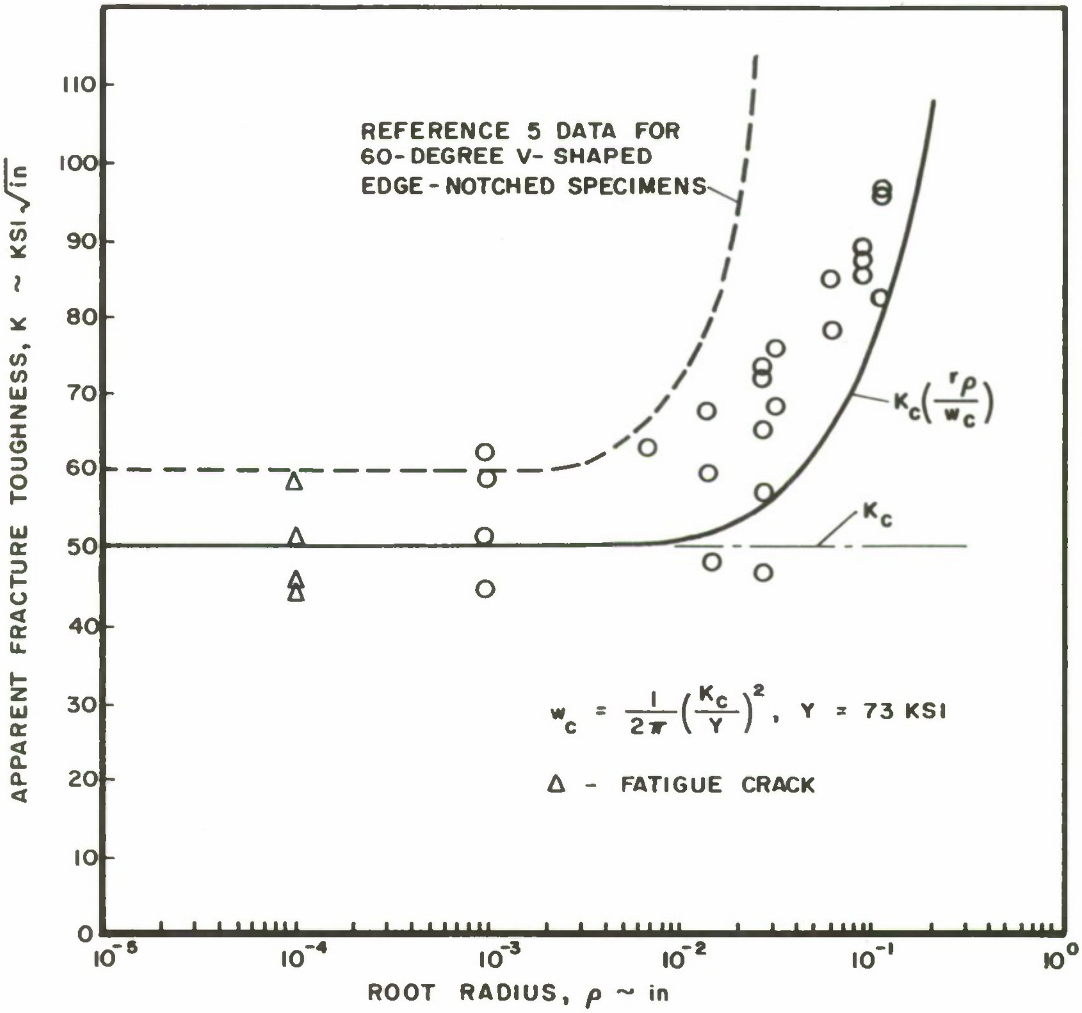

Figure 3. Variation of Apparent Fracture Toughness with Root Radius for Centrally Notched 7075-T6 Aluminum Plates

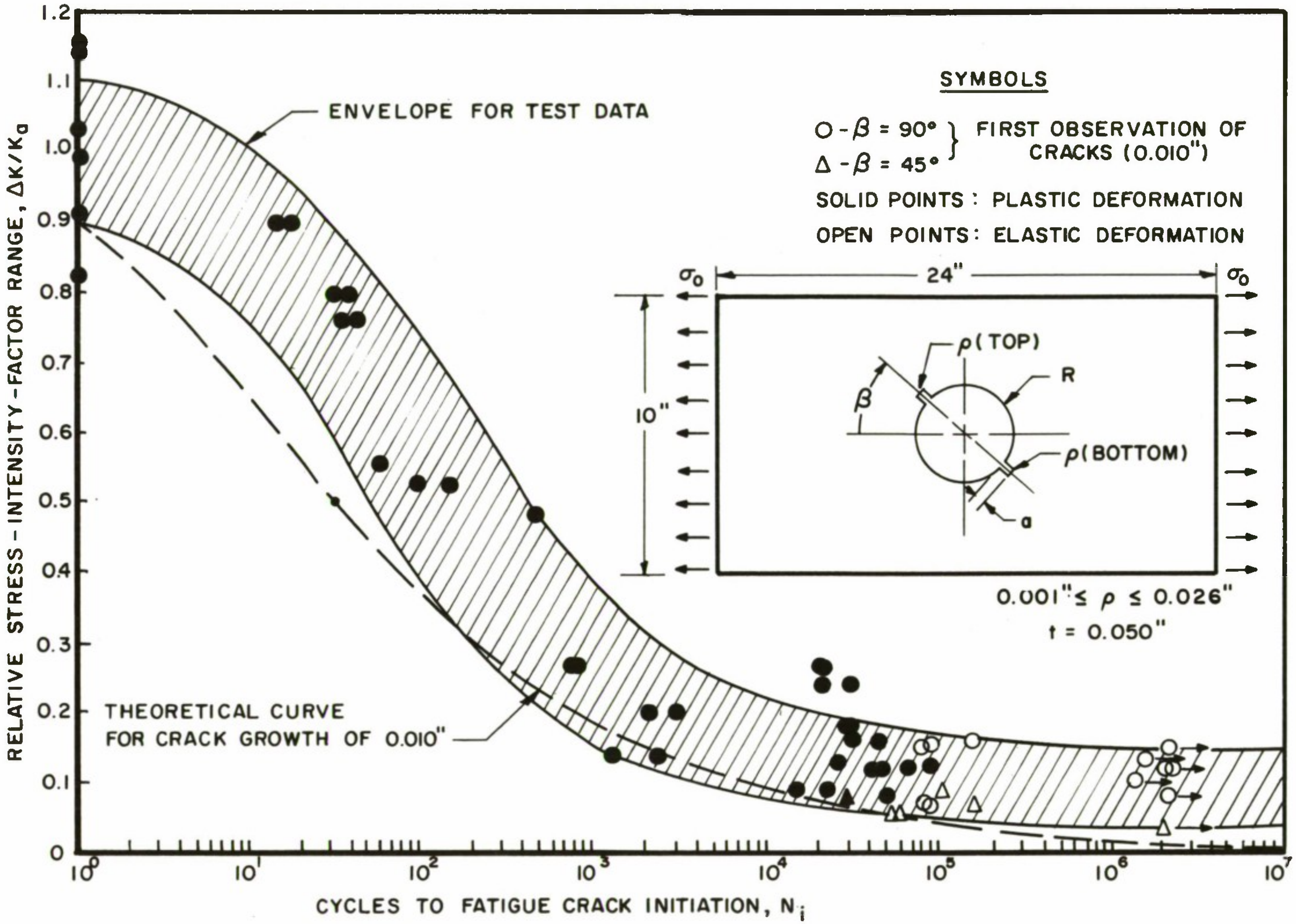

Figure 4. Comparison of Theoretical Crack Growth of .010-Inch with First Observation of .010-Inch Fatigue Cracks in Notched Sheets of 7075-T6 Aluminum

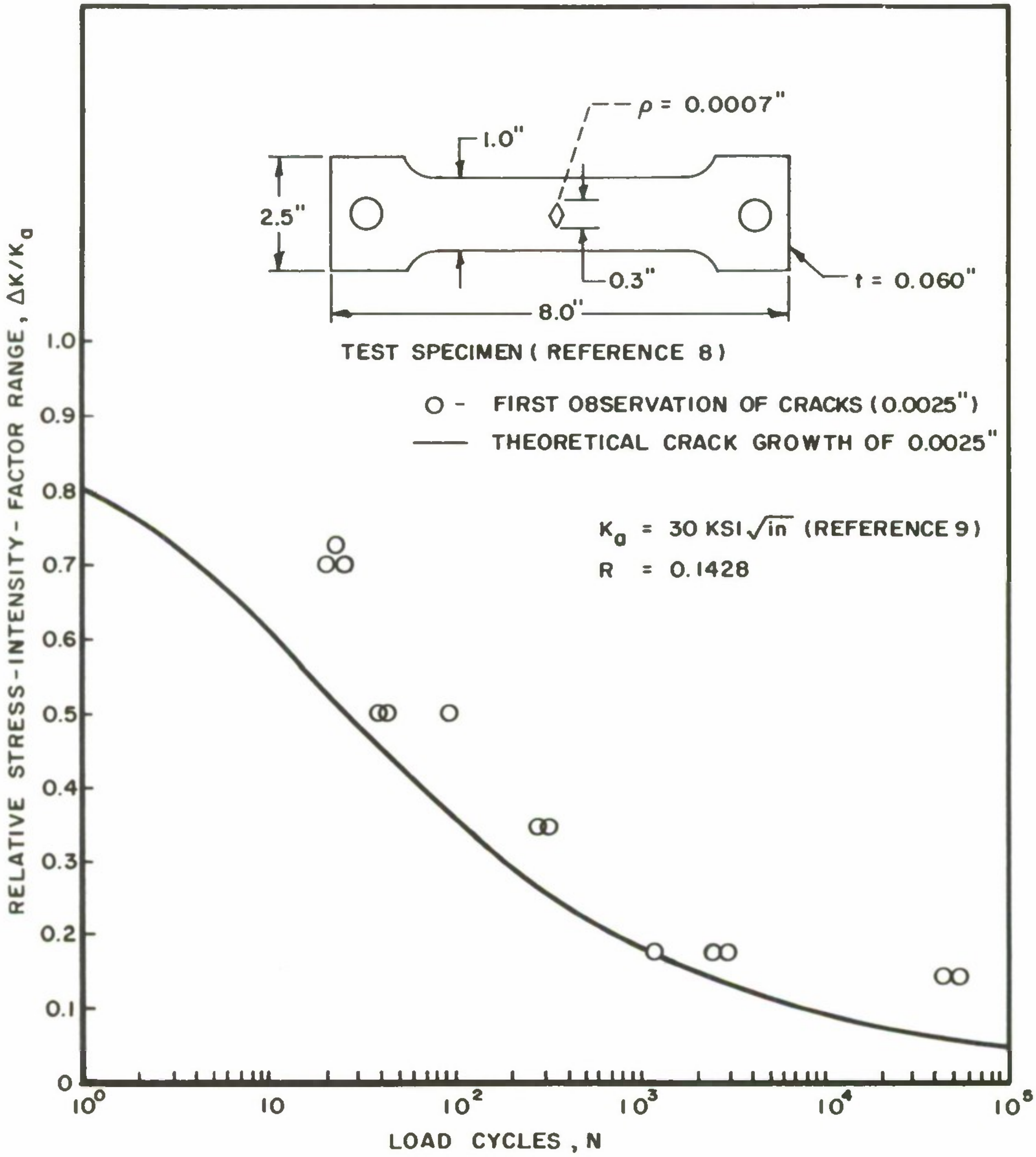

Figure 5. Comparison of Theoretical Crack Growth of .0025-Inch with First Observation of .0025-Inch Fatigue Cracks in Notched Sheets of 2014-T6 Aluminum

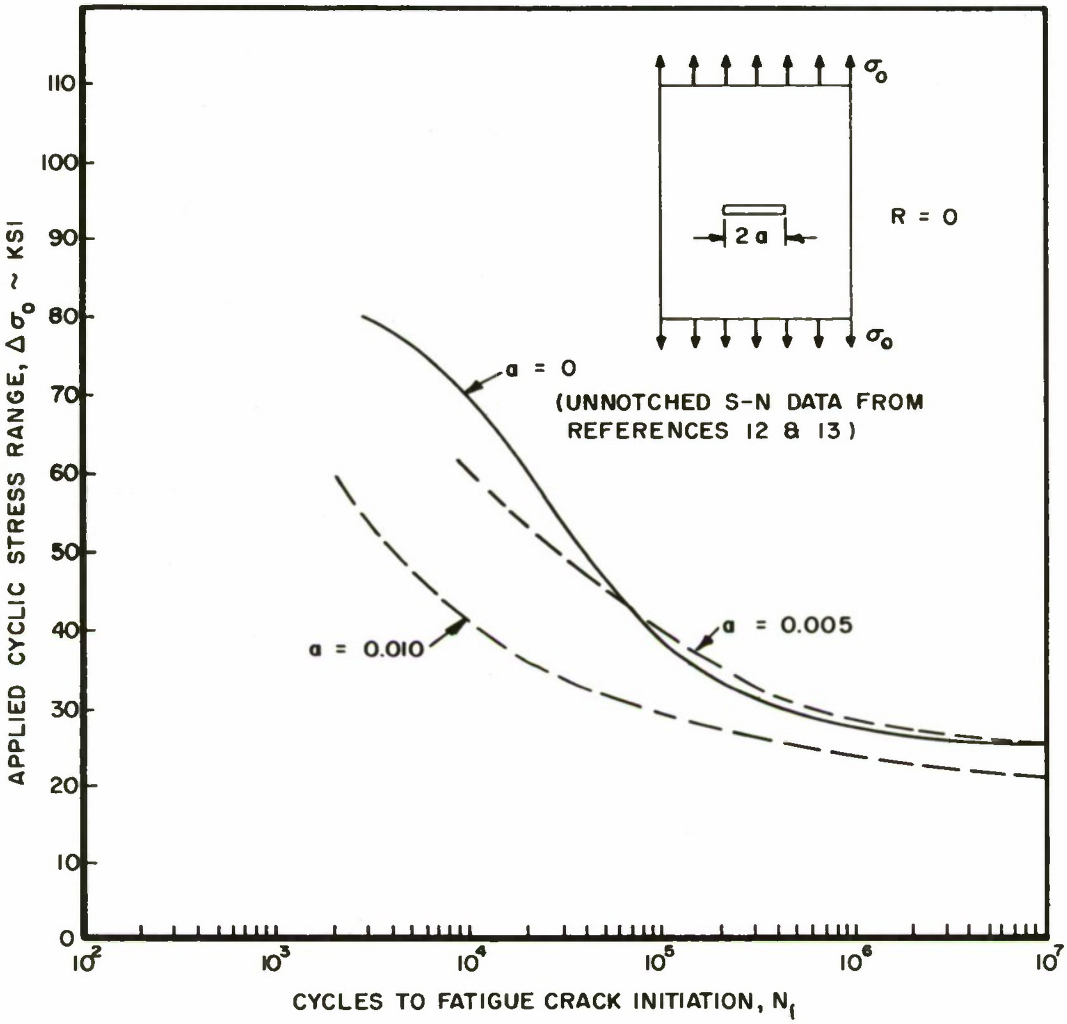

Figure 6. Comparison of Applied Cyclic Stress Range with Cycles to Fatigue Crack Initiation from Blunt Flaws in 7075-T6 Aluminum Plates

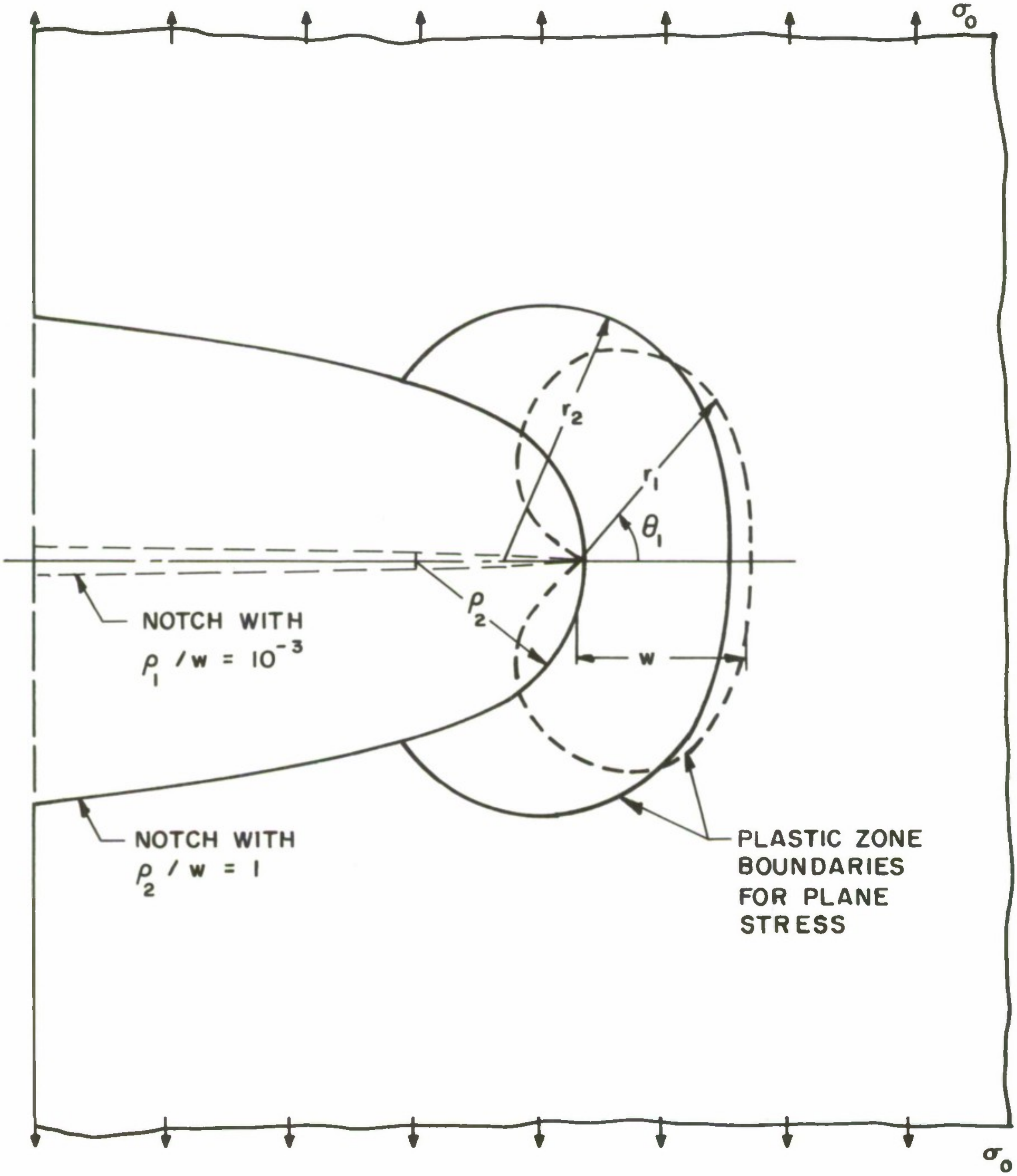

Figure 7. Geometry of Plastic Zone Boundaries at the Tip of Sharp and Blunt Elliptic Notches in a Plate Subjected to Uniaxial Tension

TABLE I

TEST DATA FOR INITIATION OF .010-INCH CRACKS IN .050-INCH-THICK 7075-T6 ALUMINUM SHEET SPECIMENS FATIGUE CYCLED AT R = 0

Specimen Number	R (in)	α (in)	β (degrees)	ρ (in)	$\Delta\sigma_0$ (psi)	N_i (cycles)****	
						Top ρ	Bottom ρ
1	1.0	0.10	90	.001	33,720	1	1
2	1.0	0.10	90	.001	18,200	60	60
3	1.0	0.10	90	.001	4,550	1,351	2,539
4	1.0	0.10	90	.026	31,000	1	1
5	1.0	0.10	90	.026	9,100	30,000	21,100
6	1.0	0.10	90	.026	18,200	470	470
7	1.0	0.10	90	.026	4,550	2 x 10^6*	2 x 10^6*
8	1.0	0.10	90	.013	31,600	1	1
9	1.0	0.10	90	.013	18,200	150	100
10	1.0	0.10	90	.013	9,100	750	800
11	1.0	0.10	90	.026	4,550	2 x 10^6*	2 x 10^6*
12	1.0	0.10	90	.013	4,550	26,372	26,372
13	1.0	0.10	90	.026	6,824	29,022	32,000
14	1.0	0.10	90	.026	6,000	81,000	81,000
15	1.0	0.10	90	.007	2,600	2.13 x 10^6*	2.13 x 10^6*
16	1.0	0.10	90	.007	6,500	2,100	3,000
17	1.0	0.10	90	.007	3,900	48,600	63,600
18	1.0	0.10	90	.007	5,200	31,500	45,000
19	0	1.15	90	.026	30,000	1	1
20	0	1.15	90	.001	30,590	1	1
21	0	1.15	90	.001	30,000	1	1
22	0	1.15	90	.026	4,800	86,400	157,430
23	1.0	0.025	90	.007	7,440	21,318	21,628
24	1.0	0.025	90	.007	4,300	2 x 10^6*	2 x 10^6*
25	1.0	0.025	90	.004	3,750	1.6 x 10^6*	1.6 x 10^6*
26	1.0	0.025	90	.002	2,810	1.4 x 10^6*	1.4 x 10^6*
27	0	1.15	90	.007	33,170	1	1
28	1.0	0.033	45	.007	10,000	137,883	**
29	0	1.15	90	.013	35,742	1	1
30	1.0	0.030	45	.007	13,230	101,943	**
31	0	1.15	90	.026	38,318	1	1
32	1.0	0.025	45	.002	14,000	29,862	49,801
33	1.0	0.028	45	.001	5,600	2 x 10^6*	2 x 10^6*
34	0	1.15	90	.031	40,000	1	1
35	1.0	0.010	45	.001	15,810	53,630	56,692
36	1.0	0.010	90	.002	6,000	88,698	75,173
37	1.0	0.010	90	.001	8,000	15,241	22,498
38	0	1.15	90	.007	20,000	36	42
39	0	0.50	90	.007	4,730	89,230	41,860
40	0	0.50	90	.007	36,000	15	18
41	0	0.50	90	.007	32,000	34	36
42	0	1.15	90	.125	50,550	1	1
43	0	1.15	90	.093	45,720	1	1
44	0	1.15	90	.125	50,716	1	1
45	0	1.15	90	.031	36,000	1	1
46	0	1.15	90	.062	44,760	1	1
47	0	1.15	90	.062	41,200	1	1
48	0	1.15	90	.093	46,700	1	1
49	0	1.15	90	.093	45,080	1	1
50	0	1.15	90	.125	43,400	1	1
16Repeat	1.0	0.110	90	***	28,830	1	1
18Repeat	1.0	0.270	90	***	25,960	1	1
22Repeat	0	1.40	90	***	24,480	1	1
38Repeat	0	1.74	90	***	19,160	1	1

* No cracks
** Not measured
*** Fatigue Crack
**** See Figure 4 for Description of Top ρ and Bottom ρ

TABLE II

THEORETICAL VALUES OF PLASTIC ZONE SIZE

ρ/w	r_p/w	
	PLANE STRESS	PLANE STRAIN*
.0001	1.0000	.2500
.001	1.0000	.2500
.01	1.0000	.2512
.02	1.0002	.2546
.04	1.0011	.2670
.06	1.0027	.2836
.08	1.0048	.3025
.10	1.0073	.3222
.12	1.0105	.3422
.14	1.0143	.3622
.16	1.0185	.3816
.18	1.0231	.4010
.20	1.0283	.4200
.30	1.0600	.5098
.40	1.0992	.5920
.50	1.1434	.6690
.60	1.1905	.7413
.70	1.2392	.8101
.80	1.2889	.8757
.90	1.3388	.9390
1.0	1.3888	1.0000
1.2	1.4883	1.116
1.4	1.584	1.226
1.6	1.679	1.332
1.8	1.772	1.432
2.0	1.8627	1.530
2.5	2.081	1.761
3.0	2.2879	1.977
3.5	2.486	2.181
4.0	2.6766	2.375
4.5	2.858	2.563
5.0	2.9975	2.742
5.5	3.206	2.915
6.0	3.3400	3.085
6.5	3.5038	3.250
7.0	3.6626	**
7.5	3.8195	**
8.0	3.9724	**

* = 0.25

** No Plastic Deformation

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13. ABSTRACT

This report presents theoretical and experimental results on fatigue crack initiation from flaws in cyclic loaded structures. The results indicated that the fracture mechanics stress-intensity-factor range, ΔK, is the governing parameter for inducing fatigue crack initiation from a flaw. Theoretical crack growth analysis indicated that when initiating an "engineering size" fatigue crack from a flaw, most of the cyclic behavior was crack growth and only a small part was nucleation.

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	ROLE	WT	ROLE	WT	ROLE	WT
Fatigue Crack Propagation Fracture Mechanics Crack Initiation Aluminum						

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